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THE PRELIMINARY DESIGN AND STATUS OF A HYDRAZINE MILLINEWTON THRUSTER DEVELOPMENT

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The hydrazine milliNewton thruster (HmNT) program was initiated to develop a thruster capable of fulfilling a dual-role in outer-planet missions requiring precision attitude control. The primary function of the HmNT is to provide micro-impulses in the range of 50 – 100 μN-sec for precise deadband pointing, precision translation of the spacecraft (very small ΔV), and/or larger impulses to remove residual rotation during a spacecraft-retargeting slew. Such precise control will help enable numerous future missions including interferometry spacecraft flying in tight formation. **This paper will discuss in detail the preliminary design, analyses, and proof of concept tests** with hydrazine that were performed with a crude catalyst bed to determine extent of the reaction. Additional efforts are planned for Fiscal Year 2000 (FY2000) pending funding.

Introduction

The primary function of the hydrazine milliNewton thruster (HmNT) is to provide micro-impulses in the range of 50 – 100 μN-sec and to remove residual rotation during a spacecraft-retargeting slew. The goals of the HmNT program are to develop a thruster that will attain a minimum impulse bit comparable to that of a cold-gas thruster, avoid the high risk of gas leakage by using a liquid propellant, approach the resulting pointing accuracy of a reaction wheel, and minimize both mass and power. The development goals broaden the capability of the HmNT as a general use thruster thereby reducing the fuel usage of a spacecraft. A small reduction in fuel usage of a spacecraft can often result in a dramatic overall wet and dry mass saving for a spacecraft. For smaller spacecraft, the HmNT could provide the entire range of attitude control function without the use of larger thrusters.

Micro-impulses in the range of 50 – 100 μN-sec can currently be achieved today by cold-gas thrusters. However, cold-gas systems are risky for long-duration missions since they suffer from gas leakage. Current Earth-orbiting and interplanetary spacecraft typically use a monopropellant (e.g., hydrazine) or a dual-mode (e.g., nitrogen tetroxide and hydrazine) system to achieve their primary propulsive requirements. Since repeatable minimum impulse bits are currently limited to ~10 mN-sec for hydrazine thrusters, spacecraft have had to use reaction wheels and/or magnetic torquers to achieve the fine pointing requirements of a mission. Since flying reaction wheels, magnetic torquers, and/or multiple propulsion systems on a single spacecraft is often prohibitive from a cost, mass, power, and/or configuration perspective, it was apparent that a micro-impulse thruster needs to be developed that could be used with conventional propulsion systems. Furthermore, reaction wheels can only provide spacecraft rotation.

For larger spacecraft, it is envisioned that the HmNT will be used in conjunction with several conventional hydrazine thrusters that are required to satisfy the larger thrust and impulse functions. Larger spacecraft might use 0.9 N (0.2 lbf) thrusters to rotate the spacecraft at a relatively fast rate. Nearing completion of this rotation, the HmNT would reduce the residual rotation to essentially the limit of detection by the guidance system. For one spacecraft study, given that pointing deadbands of the guidance system are on the order of ±1 milliradian, removal of this residual rate is anticipated to require a HmNT couple operating at the ~20 mN thrust level with each HmNT achieving a steady state firings of 30 – 60 sec. The actual firing duration will depend on spacecraft inertia and allowable time to complete maneuver. Once pointing within the deadband is achieved, the HmNTs will return to limit cycling in a "bang-bang" control mode.

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While actual requirements will be mission specific, a number of performance goals have been identified for the development effort:

- a minimum impulse bit less than 50 μN-sec
- impulse bit repeatability less than ±15% at 100 μN-sec
- a minimum steady-state specific impulse greater than 175 lbf-sec/lbm at 1.7 MPa (250 psi) inlet pressure to the thruster
- a target thrust of ~20 mN at 1.7 MPa (250 psi) inlet pressure to the thruster
- at least one steady state firing of 30 seconds per thruster
- a minimum life of 200,000 pulses per thruster
- at least 5 "cold" starts when the catalyst bed is at 20 °C (68 °F)

The following sections will describe in detail the design, analyses, and experiments that were performed in an effort to achieve the performance goals described followed by some conclusions and recommendations for further work.

Design and Analyses

This section will describe the design and analyses of the hydrazine milliNewton thruster (HmNT). The section begins with a summary of the independent design parameters. A discussion of the critical performance parameters follows. A brief summary of the design tool developed to aid in the analyses is then presented. The section ends with an overview of the different elements of the HmNT.

Independent Design Parameters

Liquid hydrazine (N_2H_4) decomposes across a catalyst bed to produce ammonia (NH_3), nitrogen (N_2), and hydrogen (H_2) according to the following two-step reaction:

$$\begin{aligned} 3N_2H_4 &\rightarrow 4NH_3 + N_2 + Q \\ 4NH_3 &\rightarrow 3N_2 + 6H_2 - Q \end{aligned} \qquad (1)$$

While the first reaction is exothermic ($+Q$), the second reaction is endothermic ($-Q$). Equation (1) can also be expressed as a function of the total amount of ammonia dissociation:

$$3N_2H_4 \rightarrow 4(1-x)NH_3 + (2x+1)N_2 + 6xH_2 \qquad (2)$$

where x is the fraction (or percent) of ammonia dissociation. The value of x can range from 0 (no ammonia dissociation) to 1 (complete ammonia dissociation).

The independent design parameters that were chosen for the HmNT analyses include:

- fraction of ammonia dissociation
- adiabatic reaction temperature
- temperature of the liquid hydrazine
- combustion chamber pressure
- nozzle area ratio
- thrust
- minimum impulse bit

The ammonia dissociation will depend on, among other things, the catalyst type and length of the catalyst bed. The adiabatic reaction temperature is based on the ammonia dissociation and enthalpy of the reaction. The temperature of the liquid hydrazine and the combustion chamber pressure are based primarily on the feed temperature and pressure, respectively, of the hydrazine coming from the upstream propellant tank(s). The nozzle area ratio was chosen to be representative of what would be used on a small hydrazine thruster. Although the thrust and minimum impulse bit are really performance outputs, they were assumed inputs in determining the other critical performance parameters. As was mentioned earlier, the target thrust and minimum impulse bit for the HmNT were 20 mN and 50 – 100 μN-sec, respectively.

Critical Performance Parameters

The overall objective of the analytical effort was to determine as accurately as possible the critical performance parameters for a HmNT. While the analyses performed assumed an ideal rocket engine these ideal analyses bound the design space for the HmNT. The seven independent design parameters described previously indirectly determine the following critical performance parameters for the HmNT:

- specific impulse
- impulse bit mass
- impulse bit volume
- mass flow rate
- volume flow rate

The following subsections briefly summarize the analysis done for each of these critical performance parameters. The equations presented can be found in any propulsion analysis textbook. They are presented here for reference.

Specific Impulse

The ideal vacuum specific impulse can be expressed as

$$I_{sp} = \frac{1}{g_0}\sqrt{\frac{2\gamma R T_c}{\gamma - 1}\left[1-\left(\frac{p_e}{p_c}\right)^{\left(\frac{\gamma-1}{\gamma}\right)}\right]} \quad (3)$$

where I_{sp} is the ideal vacuum specific impulse, g_0 is a conversion factor, γ is the ratio of specific heats of exhaust, R is the gas constant of the exhaust, T_c is the adiabatic reaction temperature, and p_e/p_c is ratio of the nozzle exit plane pressure to the chamber pressure. The ratio of specific heats and the gas constant of the exhaust depend primarily on the ammonia dissociation of the hydrazine decomposition. As a lower bound on performance, the specific impulse of simply vaporizing hydrazine (no decomposition) is 98 lbf-sec/lbm at a vapor temperature of 20 °C (68 °F).

Impulse Bit Mass

The mass required to achieve an impulse bit firing can be determined using:

$$m_{Ibit} = \frac{I_t}{I_{sp}\, g_0} \quad (4)$$

where m_{Ibit} is the mass required to achieve an impulse bit, I_t is the required impulse bit, I_{sp} is the specific impulse determined in Equation (3), and g_0 is a conversion factor.

Impulse Bit Volume

The volume of hydrazine required to achieve an impulse bit firing can be determined using:

$$V = \frac{m_{Ibit}}{\rho} \quad (5)$$

where V is the volume of hydrazine required to achieve an impulse bit, m_{Ibit} is the mass required to achieve an impulse bit determined in Equation (4), and ρ is the density of the liquid hydrazine.

Mass Flow Rate

The mass flow rate of a rocket engine can be determined using:

$$b = \frac{F}{I_{sp}\, g_0} \quad (6)$$

where b is the mass flow rate, F is the rocket engine thrust, I_{sp} is the specific impulse determined in Equation (3), and g_0 is a conversion factor..

Volume Flow Rate

The volume flow rate to the rocket engine can be determined using:

$$Q = \frac{b}{\rho} \quad (7)$$

where Q is the volume flow rate, b is the mass flow rate determined in Equation (6), and ρ is the density of the liquid hydrazine.

Design Tool

A versatile "what-if" working environment was developed in Excel to implement the analytical models. This multi-sheet Excel workbook with embedded Visual Basic programming code provided good estimates for the analytical task described. This tool was able to parametrically determine the critical performance parameters described for varying independent design parameters. As the design of the HmNT thruster progressed, this Excel tool was constantly updated and improved such that the entire analysis team had a common model to refer to.

Elements of the Hydrazine milliNewton Thruster

Although the HmNT design is not a scaled down version of a larger hydrazine thruster, the design of the HmNT shares many similarities with a larger thruster. The HmNT design can be separated into five distinct elements:

- flow control device
- valve
- propellant feed tube
- catalyst bed
- throat and nozzle

The general configuration of the HmNT and the breakout of these five elements are shown in Figure 1 below.

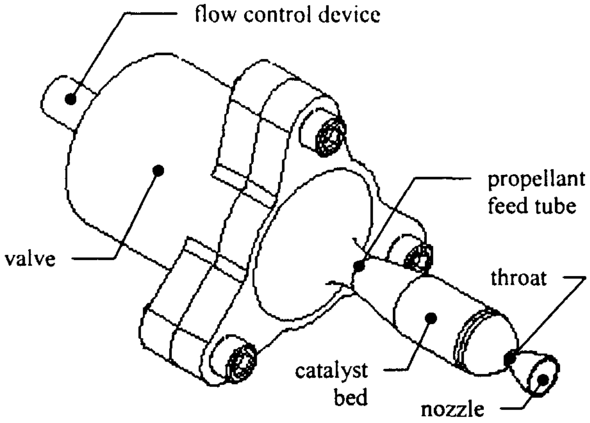

Figure 1. Hydrazine milliNewton Thruster Conceptual Drawing.

The following subsections will describe each of these elements in detail.

Flow Control Device

As was stated earlier, one of the design goals of the HmNT is to ease its integration with an existing on-board propellant system pressurized to 2.1 to 2.8 MPa (300 to 400 psi). A representative target point for the HmNT is 20 mN thrust and 150 lbf-sec/lbm specific impulse. Using Equations (6) and (7), the required hydrazine mass and volume flow rates are found to be 13.6 mg/sec and 13.5 μL/sec, respectively. To obtain such a low flow rate requires some form of flow restriction. From the standpoint of simplicity, a passive flow restriction device such as the Lee Company (Westbrook, CT) Visco Jet seems to be the most promising.

While the Visco Jet is attractive from the standpoint of simplicity and ease of integration, a concern is the potential for surges in the flow rate upon initial opening of the valve. This is particularly important for operation in the single-pulse, minimum impulse-bit mode. A preliminary water-hammer analysis was performed indicating that the surge-induced flow was less than 1% of the total volume required (0.1 μL) for a minimum impulse bit.

Another concern in controlling the flow rate is the change in the viscosity of hydrazine with temperature. The valve, and therefore the hydrazine flowing through the valve, is expected to see a wide temperature range over the different HmNT operating modes. This temperature range is estimated to be between 10 °C and 200 °C (50 and 392 °F) at the valve interface with the thruster. The Visco Jet compensates somewhat for changes in viscosity but further analysis and testing is needed.

Visco Jet

A Visco Jet manufactured by the Lee Co. was identified as a possible choice for reducing the input pressure to the HmNT valve. The Lee Co. manufactures several types of flow restrictors capable of providing flow restriction for both gases as well as liquids. In order to help the customer size the required flow restrictor they provide a handbook with the appropriate formulae and conversion factors.[1] For the case of liquids, a modified Bernoulli's equation is used to calculate the "liquid ohm" (Lohm) rating of the required restrictor. A Lohm is analogous to the electrical ohm. A one Lohm restriction will permit a flow of 6.31 L/sec (100 gallons per minute) of water with a pressure drop of 172 kPa (25 psi) at a temperature of 26.7 °C (80 °F). The Lohm rating of a device is defined as:

$$L = \frac{K\sqrt{H}}{I} \tag{8}$$

where L is the Lohm rating in units of Lohms, K is a unit constant (value 20), H is the differential pressure (ΔP) in psi, and I is the flow rate in gallons per minute. Initial calculations suggest a Lee Visco Jet with a Lohm flow resistance of $1.55 \cdot (10^6)$ should produce a pressure drop of 1.9 MPa (275 psi) at a hydrazine flow rate of 13.6 mg/sec.

Unfortunately, Equation (8) does not give any insight of the impact of viscosity on flow rate. An alternate equation, which introduces compensating factors for liquid density and viscosity, is expressed as:

$$L = \frac{K\,V}{I}\sqrt{\frac{H}{S}} \tag{9}$$

where L is the Lohm rating in Lohms, K is a unit constant (value 20), V is a "viscosity-compensating factor", I is the flow rate in gallons per minute, H is the differential pressure (ΔP) in psi, and S is the dimensionless specific gravity of the fluid. The viscosity-compensating factor is a function of the differential pressure and kinematic viscosity and is provided in the Lee Co. handbook for quick calculations. In the case of hydrazine, for temperatures at 10 and 200 °C (50 and 392 °F), the kinematic viscosities are 1.1 and 0.3 centistokes, respectively.[2] At these very low kinematic viscosities, and a modest differential pressure of 275 psi, the viscosity-compensating factor is approximately 1.0. Thus, a four-fold change in viscosity has virtually no impact on the flow rate at a given Lohm rating and pressure differential. Modest changes in the specific gravity due to temperature have a negligible impact on the flow rate. This conclusion was very encouraging as it suggested that when a Visco Jet is used in series with a capillary tube, the flow variation through the capillary tube due to viscosity effects would be significantly moderated.

An analysis was done to confirm this conclusion. In the analysis, a capillary tube with a length of 1.61 cm (0.64 inches) and diameter of 89 microns (0.0035 inches) was used. The pressure drop across the capillary tube was assumed to be 172 kPa (25 psi) and the temperatures of the tube and Visco Jet were 10 °C (50 °F). When the temperatures of the tube and Visco Jet were increased to 200 °C (392 °F), new equilibrium intermediate pressure and flow rates are established. At these conditions, the increase in flow was calculated to be less than 5%. This is in stark contrast with the case where only a capillary tube is used to drop pressure. The flow rate in such a case can increase by as much as 400% due to the viscosity decrease with temperature.

Experimental tests need to be conducted to confirm these conclusions, as there are finite uncertainties with using the Lee Company-provided viscosity-compensating factor values that form the basis of the calculations. The impact of temperature on the catalyst bed pressure profile was not considered due to its complexity. This will modify the flow rate also as it integral to the Visco Jet and capillary tube and serves as another restrictor in series; changes in catalyst bed pressures due to temperature will modify the operating pressures of the upstream restrictors. However, the changes will moderate the flow increase and hence was not considered in the analysis.

Capillary

A capillary tube can be used as pressure drop device. At the flow rates considered, the flow is in the laminar regime and hence the pressure drop per unit tube length is given by the classical Hagen-Poiseuille equation for a "very long" tube:

$$\frac{\Delta P}{L} = \frac{128 Q \mu}{\pi D^4} \tag{10}$$

where $\Delta P/L$ is the pressure drop per unit tube length, Q is the volume flow rate, μ is the viscosity of the fluid, and D is the capillary tube diameter.[3] "End-effects" can be neglected in "very long" tubes.

In the last decade, research in the area of friction and heat transfer in microchannels has resulted in better correlations for flows at very low Reynolds numbers (< 100). One such correlation was incorporated into the analysis tool for comparison with results from the classical solution provided by Equation (10). From Reference 4, a friction factor is given by:

$$f = \frac{C_{f1}}{\mathrm{Re}^{1.98}} \tag{11}$$

where f is a dimensionless friction factor, C_{fl} is a dimensionless constant (32,400) for laminar flow, and Re is the Reynolds number of the laminar flow.[4] This friction factor can then be used in:

$$\frac{\Delta P}{L} = \frac{1}{2}\rho \left(v_{avg}\right)^2 f \frac{1}{D} \tag{12}$$

where $\Delta P/L$ is the pressure drop per unit tube length, ρ is the density of the fluid, v_{avg} is the average velocity of the fluid, and D is the capillary tube diameter. Figure 2 shows the required capillary tube diameter to obtain a desired pressure drop per unit length.

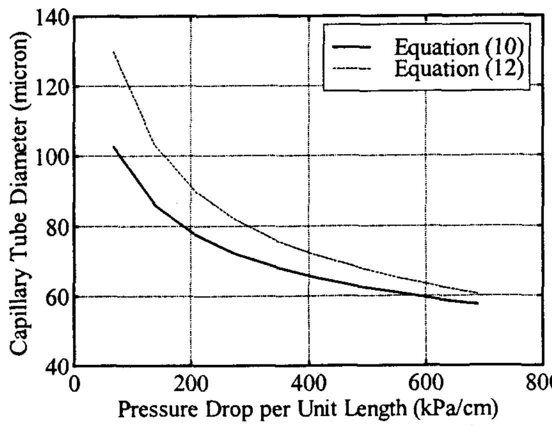

Figure 2. Capillary Tube Diameter as a Function of Pressure Drop per Unit Length.

The two curves in Figure 2 represent the results of using Equations (10) and (12) assuming a flow rate consistent with a thrust of 20 mN and a specific impulse of 100 lbf-sec/lbm. These results suggest that obtaining the needed pressure drop (~2 MPa or 300 psi) solely through the use of a capillary tube could be done in principle although the required length (several cm) or diameter (<100 microns) would increase the risk of plugging.

For applications where the temperature is expected to remain constant, the capillary tube may be sufficient to provide the required pressure drop as noted above. However, in the case of the HmNT where the temperature can range from 10 to 200 °C (50 and 392 °F), a four-fold reduction in hydrazine viscosity causes a four-fold increase in flow rate. This is not acceptable for the tight impulse-bit requirements of the HmNT. However, when the capillary tube of the feed injector is used with a pressure drop device such as the Visco Jet discussed in the previous section, the viscosity-related problem is alleviated significantly.

Two-Phase Plenum

Another option evaluated for propellant flow control of the HmNT was the two-phase plenum (TPP). The objective of the TPP was to provide a system capable of vaporizing the hydrazine propellant upstream of the HmNT in order to provide a controlled, low-pressure gas supply to the thruster to achieve lower thrust levels as well as smaller and more repeatable impulse bits. A single TPP could be sized to support multiple thrusters in order to minimize overall system mass. The basic operation of the TPP consists of controlling the temperature of a volume that contains propellant as a mixture of liquid and vapor phases in thermal equilibrium. The extracted vapor used during a thruster firing is therefore supplied at the vapor pressure of the mixture for a given plenum temperature which is controlled within some specified tolerance. As the gas is used up, equilibrium drives more of the liquid towards vapor at a constant temperature and pressure until no liquid is left. At that point, only gas remains, and the supply pressure would then decrease if gas continued to flow out of the plenum. In the present concept, one supply cycle consists of a "charging up" of the plenum, storage until needed, and then supply of vapor during a burn until there is no further liquid phase remaining. Figure 3 shows a conceptual schematic of the TPP.

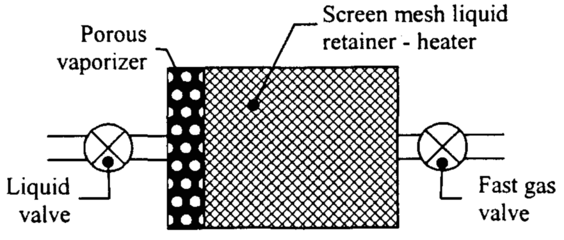

Figure 3. Schematic of a Two-Phase Plenum.

On the left side of Figure 3 is the high-pressure fluid supply with a liquid valve. This valve provides a controlled amount of liquid to the plenum volume when recharging the TPP. The plenum volume is envisioned to be cylindrical although the exact shape will be determined by heat transfer considerations. Immediately upon entering the plenum, the liquid is heated as it passes through a heated porous vaporizer. This initial vaporizer will not vaporize all of the liquid but serves to provide a pressure drop preventing a high-pressure liquid jet from streaming into the plenum. It also serves to prevent evaporative freezing of the liquid at the entrance of the plenum. Downstream of the porous vaporizer, the plenum is packed with a high-surface area mesh. This mesh serves first to entrain the liquid and prevent it from travelling downstream much like a propellant management device, and secondly to enable good thermal conductivity between the heated walls of the plenum and the propellant within. Downstream of the plenum are a fast gas valve and the thruster.

The key scaling parameter for the TPP is the length of burn time one must support between recharges. Assuming that the plenum has just been recharged at the beginning of a cycle, the plenum contains the recharge supply propellant plus whatever residual propellant (as vapor) was left from the previous cycle. This total propellant mass rapidly reaches some equilibrium mixture of part vapor and part liquid:

$$m_1 = m_{l1} + m_{v1} \tag{13}$$

where m_1 is the total propellant mass, m_{l1} is the recharge supply propellant mass, and m_{v1} is the residual (vapor) propellant mass. The mass available for a burn period is determined by the point at which no liquid phase remains in the plenum. For a plenum of volume with an open volume fraction (determined by the mesh and porous heater), the total volume available for vapor is:

$$V_t = \phi V_p \tag{14}$$

where V_t is the total volume available for vapor, ϕ is the dimensionless open volume fraction, and V_p is the plenum volume. The residual mass left in the plenum at the end of a burn can then be calculated from the ideal gas law, which is applicable at the low temperatures and pressures of interest:

$$m_2 = \frac{p_{vap} V_t}{R T_{vap}} \tag{15}$$

where m_2 is the residual mass left in the plenum, p_{vap} is the vapor pressure of the fluid, V_t is the total volume available for vapor, R is the gas constant of the fluid, and T_{vap} is the temperature of the fluid vapor. The mass available for a burn can be expressed in terms of the plenum size and gas conditions by taking the difference of Equations (13) and (15):

$$\Delta m_b = m_l \left(1 - \frac{p_{vap} V_t}{m_1 R T_{vap}} \right) \quad (16)$$

where Δm_b is the mass available for a burn, m_l is the total propellant mass, p_{vap} is the vapor pressure of the fluid, V_t is the total volume available for vapor, R is the gas constant of the fluid, and T_{vap} is the temperature of the fluid vapor. The mass available for a burn, which must be injected between cycles, can also be expressed as a function of the burn time, the thrust and specific impulse.

$$\Delta m_b = \frac{F \Delta t_b}{I_{sp} g_0} \quad (17)$$

where Δm_b is the mass available for a burn, F is the engine thrust, Δt_b is the burn time, I_{sp} is the specific impulse, and g_0 is a conversion factor.

The primary advantage of the TPP is the ability to supply 100% vapor to the thruster at low feed pressures. This is expected to result in smaller, more repeatable impulse bits. Using a vapor supply to the HmNT could lower the bottom end of the thrust range by an order of magnitude without requiring excessively small diameter passages. One of the disadvantages of the TPP approach is the power required to convert liquid to vapor electrically. It is much more efficient to do this through chemical means using the catalyst bed if low-flow and repeatability issues can be addressed. Other disadvantages include system complexity, additional hardware, and associated temperature control loops. This could be outweighed however by the operational flexibility of changing the plenum temperature, and hence the supply pressure and thrust level during a mission for throttling purposes as needed.

Valve

In January 1999 a contract was issued for a fast-acting, low-power valve that would support the response time characteristics needed for the HmNT. Of the five proposals received, three were judged to be competitive. After on-site evaluations, EG&G Wright Components (Phelps, NY) was selected as the contractor. Even though this is a new valve design with a significant amount of development, the design and supporting analyses provide a fair amount of confidence in the following performance predictions:

- Opening response time of less than 0.5 ms at 28 V_{DC}, 20 °C (68 °F), and 2.8 MPa (400 psia) inlet
- Closing response time of less than 1.0 ms at 28 V_{DC} with a 14 V EMF suppression diode
- Operating power less than 5 W at 28 V_{DC} and 12 ° C (54 °F), minimum temperature
- Total mass of less than 20 grams
- Demonstrated life greater than 300,000 cycles
- TBD valve seat material, probably AFE-411 or Teflon

The valve will go through a qualification test program including shock, random vibration, and thermal cycling with functional performance tests (including mass flow rate and pressure drop) performed at the high and low temperature extremes of the first and last thermal cycles for a different application.

For the HmNT, the flow restrictor represented in Figure 1 will need to be added and closely coupled to the valve seat to minimize the "surge induced flow" that was previously described. Having the minimum fluid volume possible between the restrictor and the valve seat will probably result in the flow restrictor being made an integral part of the valve.

Feed Tube

The feed tube of the HmNT is the passage between the valve seat and the inlet to the catalyst bed. The design of the feed tube is critical in developing a thruster capable of delivering small minimum impulse bits and mitigating high heat soakback. The primary functions of the feed tube are:

- To provide a large enough pressure drop between the valve seat and the catalyst bed to mitigate the pressure oscillations between the catalyst bed and the valve. This pressure drop also provides a small part of the overall flow restriction needed to achieve the flow rate corresponding to a thrust level of 20 mN.
- To provide some amount of thermal standoff between catalyst bed and the valve seat.

The desire to obtain thermal isolation and an increased pressure drop suggest the use of a longer feed tube. However, an excessively long and/or narrow feed tube introduces several additional problems. For operation in minimum impulse bit mode, the required volume of propellant is exceedingly small. Using Equations (4) and (5), the required hydrazine volume is found to be ~0.1 μL for a minimum impulse bit of 100 μN-sec and a specific impulse of 100 lbf-sec/lbm. It was assumed that for repeatable impulse bits with reasonable tail-off times the total "dribble volume", defined to be the total volume between the valve seat and the entrance to the catalyst bed, should be small relative to the propellant volume needed for the minimum impulse (~0.1 μL). Minimizing the dribble volume while insuring adequate thermal isolation is one of the most challenging aspects of the development.

As illustrated in Figure 2, the classical Poiseuille solution provides a more conservative result (smaller diameter feed tube) and has been assumed in the analysis. For a capillary inlet tube inner diameter of 75 microns with a pressure drop of 117 kPa (17 psi) requires a length of ~5 mm. This equates to a dribble volume equal to 22% of the 0.1 μL minimum impulse bit volume. Figure 4 illustrates the ratio of the dribble volume to the propellant volume required for a single impulse bit as a function of the feed tube diameter.

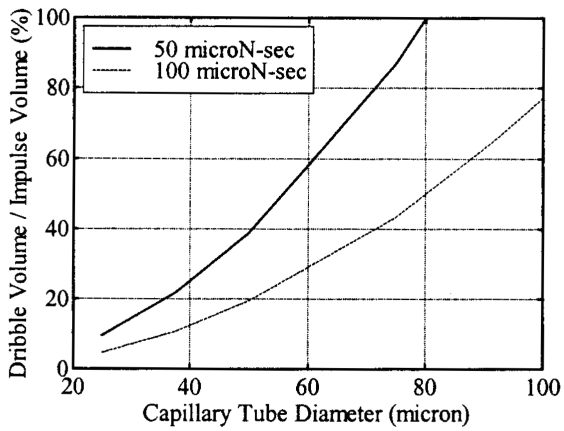

Figure 4. Ratio of Dribble Volume to Propellant Volume Required for a Single Impulse Bit as a Function of the Feed Tube Diameter.

These curves were generated for an assumed tube length of 1 cm, a specific impulse of 100 lbf-sec/lbm, and are shown for two bounding values of impulse bit. If one considers a 5 mm tube length as in the previous example, then the requirement on tube diameter given in Figure 4 can be relaxed (diameter increased) by a factor of 1.4 while maintaining the same dribble-to-impulse propellant volume ratio.

The use of a smaller diameter feed tube increases the pressure drop that can be obtained for a given length as well as reducing the dribble volume. However this also has the effect of worsening the problem of clogging the feed tube with non-volatile residue (NVR) during operation. Although plugging of the feed tube with particulates could be a problem, it was assumed that this issue could be addressed in the future with proper filtration at the inlet to the valve and a "no-sliding-fit" valve design.

Achieving a balance between the need to provide adequate thermal standoff, minimizing the dribble volume of the feed tube, and allowing the internal diameter of the feed tube to be large enough that NVR will not plug the feed tube over 300,000 pulses is a difficult challenge. Another approach assumed a hard seat valve and accepting a dribble volume that is large relative to the propellant volume needed for minimum impulse bit. Although easing certain aspects of the feed tube problem, each of these two approaches introduces additional problems.

Using a hard seat valve would allow higher temperatures at the valve seat. However, higher temperatures along the feed tube, particularly at the inlet to the catalyst bed, would exacerbate the NVR deposition problem. The NVR deposition problem, and not the valve seat temperature, may well be the dominant problem. For the moment, the hard seat valve option was not assumed because of the risks associated with developing a hard seat design that can withstand 300,000 pulses and remain leak tight. Furthermore, higher seat loads and stronger valve coils (higher coil power and larger coil volume) are needed to support a hard seat design.

Accepting a dribble volume that is large relative to the propellant volume needed for the minimum impulse would result in minimum impulse bits that would have less of a "pulse" characteristic and a more of a spread out low-pressure characteristic. This may undesirable due to the longer impulse time. However, this approach may have merit and will be investigated further.

Catalyst Bed

Design of the catalyst bed for the HmNT is another significant challenge in the development activity. Initial sizing work attempted to use derived scaling equations performed by Rocket Research Corp., now PRIMEX Aerospace Corp. (Redmond, WA).[5] However, the very-low Reynolds numbers characteristic of the HmNT flow rates makes the use of these derivations suspect at best. The length of the reaction chamber may be driven by the desire to minimize ammonia dissociation in order to maximize the adiabatic reaction temperature, which in turn maximizes the specific impulse. The low flow rate required for the HmNT (on the order of microliters per second) also has implications for the catalyst bed. The decomposition of the hydrazine vapor in the catalyst bed will occur at a finite rate with a characteristic propagation velocity for the reaction front. While the length of the catalyst bed is partly determined by the desired level of ammonia dissociation, the diameter will be dictated in part by the desire to insure the average axial flow velocity is greater than the flame propagation velocity. The optimal steady state chamber operating pressure has not yet been determined but is expected to be on the order of several hundred kilopascals. At pressures greater than atmospheric, the propagation velocity for the decomposition front scales inversely to the square root of the pressure.[6] Figure 5 shows the ideal thrust as a function of chamber pressure for three representative catalyst bed diameters.

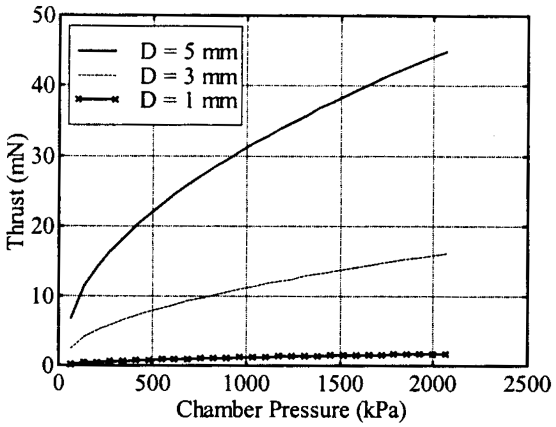

Figure 5. Thrust as a Function of Chamber Pressure and Catalyst Bed Diameter.

The calculations illustrated in Figure 5 assumed a hydrazine vapor temperature of 150 °C (302 °F), a specific impulse of 175 lbf-sec/lbm, a catalyst bed porosity of 16%, and were constrained such that the ratio of the mean flow velocity to the reaction front velocity is equal to one. The mean flow velocity assumes the flow is through an open cross-sectional area equal to the product of the physical cross-sectional area (as determined by the listed diameter) multiplied by the porosity. This is undoubtedly an oversimplification since the flow passages within the catalyst bed are not likely to be uniform. Nonetheless, this calculation provides a useful bound. The results also underscore the conclusion that the low-thrust level of the HmNT requires not just low flow rates, but relatively low-chamber pressures. This fact in turn has implications for overall sizing of the catalyst bed in order to maintain stable decomposition.

Reference 5 derives scaling relationships for the catalyst bed length as a function of ammonia dissociation fraction. As was mentioned earlier, the low Reynolds number characteristic of HmNT flow rates probably makes the use of these relations unreliable. Hence, a catalyst bed length that is 1.5 to 2.5 times the catalyst bed diameter has tentatively been assumed.

A number of internal structures and geometries have been discussed for the catalyst bed. Current tests have proceeded with Shell 405 granules of 20 to 30 mesh size. The extremely narrow capillary passage at the capillary inlet tube makes the issue of non-volatile residue (NVR) formation a serious concern.

Throat/Nozzle

To date, the throat/nozzle of the HmNT has received the least attention of the five HmNT elements. For a chamber temperature of 264 °C (507 °F), a chamber pressure of 345 kPa (50 psi), and assuming a value for the ratio of specific heats of 1.32, the corresponding ideal isentropic ratio of mass flow rate to throat area is approximately 417 kg/sec-m^2. To sustain a mass flow rate of 13.6 mg/sec, consistent with a thrust of 20 mN and a specific impulse of 150 lbf-sec/lbm, requires a throat diameter of ~204 microns. Assuming an expansion ratio of 100:1 results in an exit plane nozzle diameter of only 2.04 mm. Too great a divergence angle in the nozzle results in flow separation and viscous losses. A shallow angle with a longer nozzle results in cooling of the flow with minimal thermal to kinetic energy recovery. For these reasons, the relatively low values of specific impulse assumed (< 150 lbf-sec/lbm) do not assume any significant acceleration in the nozzle.

Experiment

Initially, two phases of experiment/test were planned with design iteration and fabrication of new hardware between each phase. The primary goal of Phase I was to determine the extent of hydrazine decomposition. Phase II would focus on obtaining more accurate fundamental measurements of the flow rate, temperature, and pressure of the catalyst bed.

To determine the extent of hydrazine decomposition for Phase I, a simple hardware set-up was used that pulsed very small amounts of hydrazine into a "crude catalyst bed". Prior to these tests, there were some concerns about the ability to get hydrazine decomposition without optimizing parameters such as catalyst bed sizing, geometry, preheat temperature, injector design, and chamber pressure. One of the fundamental questions that this experimentation was to answer was whether these parameters are critical to getting good decomposition at these extremely small hydrazine throughputs or were these parameters "merely" performance enhancements. The answer could help guide future experiments and help determine how best to apply limited resources.

Figure 6 illustrates the test setup that consisted of a pressurized hydrazine supply bottle; an optional sight glass flow path; and a combination filter and 2 million Lohm Visco Jet flow restrictor close coupled to the inlet of a fast actuating solenoid valve.

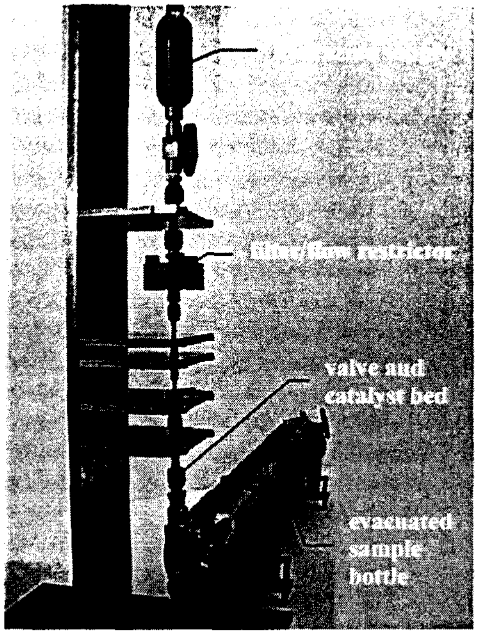

Figure 6. MilliNewton Thruster Test Set-up.

Downstream of the solenoid valve was a thermal standoff leading to a 76.2 micron diameter by 254 micron length (0.003" by 0.010") orifice at the entrance to the catalyst bed. The catalyst bed chamber had the throat sized to provide a chamber pressure of ~138 kPa (~20 psia). Rather than a true nozzle, the exit from the throat was a crude nozzle shape ending in a 6.35 mm (1/4") tube that led to an evacuated sample bottle. Since no direct measurements of thrust or even chamber pressure measurements were planned for this phase, the primary method of determining hydrazine decomposition was to perform mass spectrometer analysis on the gases captured in the evacuated sample bottle. The sample bottle was thoroughly cleaned and dried beforehand, then maintained at an elevated temperature to prevent condensation of potentially undecomposed hydrazine. The feed system was pressurized to ~345 kPa (~50 psia). The catalyst bed was preheated to ~150 °C (~300 °F). Although accurate command/measurement of valve on-times were not possible with the data system used for these tests, the hydrazine throughputs per pulse ranged between ~0.5 to ~4 μL per pulse as read by sight glass measurements. If a specific impulse of 100 lbf-sec/lbm was assumed, this would equate to impulses in the range of ~0.5 to 4 mN-s.

Samples of the products of several short pulses were collected in the evacuated sample bottle. The JPL Chemistry Laboratory analyzed three such samples with a mass spectrometer:

Table 1. MilliNewton Thruster Test Results.

	Sample		
Products (Mole %)	1	2	3
Nitrogen (N_2)	45	37	22
Hydrogen (H_2)	2	3	39
Ammonia (NH_3)	42	48	32
Water vapor (H_2O)	3	4	6
Methane (CH_4)	5	4	<1
Hydrazine (N_2H_4)	3	4	<1

The uncertainty of the mass spectrometer is believed to be on the order of ±10% of the reported value. For samples 1 and 2, the percent of nitrogen is too high and the percent of hydrogen is far too low for what would be expected from a hydrazine thruster. The reason for these unusual readings is unknown. It is possible that residuals, either from the vacuum pump or cleaning products, remained in the test set-up during samples 1 and 2 and were flushed out by the sample 3. Sample 3 appears to be the only test with reasonable results. Nonetheless, it was encouraging to learn from these three samples that even with our "crude catalyst bed", there was very little undecomposed or "raw" hydrazine. These results indicate that it is possible to get significant testing done without optimizing most of the design parameters of a miniature hydrazine thruster.

Temperature measurements were made on 4 locations of the catalyst bed. However, due to the relatively large thermal mass of the hardware, these measurements provided only qualitative information, averaging ~4 to ~6 °C (~7 to ~11 °F) increase per pulse.

Unfortunately, funding for the HmNT development was cut by the customer mission Europa in fall 1999 during Phase I of the experimentation. Europa's science team became convinced that during gravity mapping phase of the mission (in orbit around Europa), they needed near zero residual ΔV while maintaining spacecraft attitude, and concluded that this could only be achieved by reaction wheels. Calculations performed indicated that even the very small residual ΔV imparted to the spacecraft by HmNT couples was more than the gravity mapping measurements could tolerate. The Project management was also concerned about the cost and schedule risk associated with the HmNT development. Given the nature of this development and the maturity of the technology, the HmNT development manager agreed that the development was risky given the very ambitious schedule and budget arrangements. Due to the funding shortfall, Phase II of the planned experiments was not completed.

Conclusions

This paper discussed in detail the preliminary design, analyses, and proof of concept tests that were performed with a hydrazine milliNewton thruster (HmNT). One of the significant conclusions that was reached early on in the analyses was that scaling the existing 0.9 N (0.2 lbf) hydrazine thruster would probably not work down to the ~20 mN thrust level. To obtain the low thrust and minimum impulse bit requirements needed resulted in too low a chamber pressure or too small a throat diameter than is currently practical. The design of the propellant feed system and catalyst bed appears to be the most challenging aspect of developing an HmNT. Easing of the thrust, minimum impulse bit, and multifunctionality requirements of the HmNT would ease many of the challenges associated with the baseline approach described in this paper.

Recommendations

A significant amount of design and analyses has been spent in developing a hydrazine milliNewton thruster (HmNT). Unfortunately, funding was cut to the HmNT development early in the testing program. When funding is available to continue this design work, it is recommended that the following items be completed:

- Testing of non-volatile residue plugging on microtubes of various sizes
- Additional analysis and system sizing for the two-phase plenum concept
- Fabrication and testing of a miniature thruster with measurements of the chamber pressure, temperature at various critical locations, throughput bits, and command valve operations
- Building and integration of a performance and thermal model
- Iterating a series of testing and analyses

Investment in these areas will significantly contribute to the development of a multifunctional HmNT.

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